

7. An Analysis of Pilot Adaptation in a Simulated Multiloop VTOL Hovering Task

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Human pilot adaptation in a simulated multiloop VTOL (vertical takeoff and landing) hovering task was investigated using a series-loop closure model. Using the model equations, the pilot model adaptable parameters were computed from rms hovering performance data measured in flight simulator experiments for a variety of VTOL aircraft configurations. Variations in the aircraft stability parameters affected both the dynamic and disturbance-response characteristics of the controlled element.

The pilot's pitch-loop adaptation generally correlated with the frequency domain characteristics of the pitch response to turbulence and the pitch response to control inputs. Gain was adapted to provide a crossover frequency that was high enough to control the disturbances due to turbulence and provide satisfactory hovering performance. Lead compensation was adapted in the region of crossover frequency to provide closed-loop stability. The results of the study show no low-frequency attitude gain requirements that must be satisfied for the attitude disturbance and dynamic characteristics considered. The influence of the aircraft position-loop characteristics on pilot pitch-loop adaptation was important only for configurations that had small attitude disturbances. For these cases the requirements for position-loop control apparently determined the minimum acceptable pitch-loop crossover frequency. Results also show that the pilot preferred to adjust pitch-loop gain by changing control stick sensitivity (aircraft gain) rather than by changing his internal adapted gain.

INTRODUCTION

Through intelligent design and application of stability augmentation systems, advanced vehicles such as VTOL aircraft, lifting reentry bodies, orbiting space vehicles, and lunar landing modules can be made to have control characteristics that are acceptable to most pilots. However, the margin for error in initial stability augmentation system design decreases as the flight vehicles and tasks become more complex. A better understanding of human pilot control characteristics is necessary. In particular, a practical mathematical definition of human pilot control characteristics would be an asset to the designer.

A number of studies have been conducted with the objective of mathematically modeling pilot control characteristics (for example, refs. 1, 2 and 3). From a practical engineering point of view, the development of the quasi-linear model for human pilot dynamics has been the most beneficial outcome of these studies (ref. 1). The quasi-linear model describes pilot dynamic control characteristics in terms of frequency-domain servoanalysis parameters, i.e., adaptable gains, leads and lags, and a relatively constant neuromuscular lag and transport lag. Qualitative rules defining the proper adjustment of the adaptable model parameters to simulate pilot adaptation have also been developed in these studies.

In the development of the quasi-linear pilot models, most studies have been limited to pilot control in a one-degree-of-freedom task. In many flight vehicles, however, the most fundamental task is the control of two coupled degrees of freedom. An example of this is the control of pitch attitude and longitudinal hovering position for a VTOL aircraft. Because the pilot is limited in his control capabilities, one would not necessarily expect that the rules governing pilot adaptation in one-degree-of-freedom control tasks would also apply to two-degree-of-freedom control tasks. Pilot adaptation in two-degree-of-freedom tasks is, therefore, an area that requires additional study.

A previous study of the characteristics of pilot dynamics in a two-degree-of-freedom task was conducted at the United Aircraft Research Laboratories (UARL). The objective was to determine the correlation between pilot opinion, optimum control sensitivity, and computed pilot adapted parameters (ref. 4). A two-degree-of-freedom simulated VTOL aircraft hovering task was modeled as a multi-closed-loop system in which pilot dynamics were represented with quasi-linear models. The pilot adapted parameters (gain and lead) were computed from rms hovering performance data measured in the UARL V/STOL aircraft flight simulator. Pilot opinion was found to correlate with pilot adapted lead and pilot workload (as indicated by the aircraft response to turbulence). The pilot's tendency to maintain constant adapted pitch-loop gain for a wide range of configuration parameters apparently was the criterion used to select optimum control sensitivity. The results of the aforementioned study suggest the possibility of analytically predicting pilot opinion and optimum control sensitivity. If the adapted pilot model parameters and the characteristics of aircraft response to turbulence can be estimated, an estimate of pilot opinion and optimum control sensitivity might be made. However, the usefulness of this analytical approach depends upon the reliability of the estimated pilot-adapted parameters. The criterion governing pilot adaptation in multiloop control tasks must be understood to properly define these parameters.

This present study was undertaken to explore the factors that influence pilot adaptation for a simulated two-degree-of-freedom hovering task in turbulent air.

SYMBOLS

dB	decibels, $20 \log_{10} ()$ where () is an amplitude quantity, e.g., $\theta(s)/\delta_e(s)$
g	gravitational constant, 32.2 ft/sec ²
I_y	moment of inertia in pitch, slug-ft ²
j	$\sqrt{-1}$
$K_{P\theta}$	pilot adapted pitch-loop gain, in./rad
K_{Px}	pilot adapted position-loop gain, rad/ft
m	mass of aircraft, slugs

M_q	pitching moment due to pitch rate divided by I_y , $1/\text{sec}^2$
M_u	pitching moment due to longitudinal velocity divided by I_y , $1/(\text{ft-sec})$
M_{δ_e}	pitching moment due to unit longitudinal control stick displacement divided by I_y , $(\text{rad/sec}^2)/\text{in.}$
M_θ	pitching moment due to pitch attitude divided by I_y , $1/\text{sec}^2$
PM	phase margin, an indicator of closed-loop stability expressed as the phase lag at crossover frequency of the aircraft open-loop dynamics and the pilot model plus 180° , deg
q	pitch rate, rad/sec
s	Laplace operator, $1/\text{sec}$
S_{u_g}	turbulence power spectrum, ft^2/sec
T	constant defining first-order root location in the complex plane, sec
T_{L_x}	pilot adapted position-loop lead, sec
T_{L_θ}	pilot adapted pitch-loop lead, sec
T_N	neuromuscular lag, sec
u	velocity component in x direction of stability axis system, ft/sec
u_g	longitudinal component of gust velocity, ft/sec
x	longitudinal coordinate in stability-axis system, ft
X_u	longitudinal force due to longitudinal velocity divided by m , $1/\text{sec}$
Y_{P_θ}	pitch-loop quasi-linear pilot model (see eq. (3)), in./rad
Y_{P_x}	position-loop quasi-linear pilot model, rad/ft
δ_e	longitudinal displacement of control stick from center of travel, in.
ζ	damping ratio of oscillatory roots (see eqs. (4) and (5))
θ	pitch attitude relative to horizon, rad
θ_x	position-loop command angle, rad (see fig. 2)
$\theta(s)/u_g(s)$	transfer function relating pitch attitude to turbulence (see table 1), rad-sec/ft
$\theta(s)/\delta_e(s)$	transfer function relating pitch attitude to control inputs (see table 1), rad/in.
σ_q	RMS pitch rate, rad/sec
σ_u	RMS longitudinal velocity, ft/sec
σ_{u_g}	RMS longitudinal turbulence, ft/sec
σ_x	RMS longitudinal hovering position error, ft
σ_θ	RMS pitch attitude, rad
τ_θ	pitch-loop pilot model transport lag, sec

τ_x	position-loop pilot model transport lag, sec
ω	frequency, rad/sec
ω_B	break frequency of first-order filter for generating turbulence, rad/sec
ω_d	damped frequency of oscillation, rad/sec
ω_n	natural frequency of oscillation, rad/sec

Superscript:

denotes first derivative with respect to time, d/dt

Subscripts:

REF denotes reference or base value of a variable

x, θ denotes quantities pertaining to position and pitch loops, respectively

e denotes error between desired and actual value of a variable

SIMULATED VTOL HOVERING TASK

Brief discussions of the equations of motion, the simulated turbulence, the UARL V/STOL aircraft flight simulator, and methods used in performing the experimental portion of this study are presented here. More detailed discussions are contained in reference 4.

EQUATIONS OF MOTION.—The general form of the linearized equations of motion that describe the pitch attitude (θ) and longitudinal position (x) responses of the simulated hovering VTOL aircraft to control inputs and to turbulence are

$$\begin{aligned} M_u \dot{u} + M_\theta \dot{\theta} + M_q \dot{\theta}^I &= -M_{\delta_e} \delta_e - M_u u_g \\ X_u u - g \cos \theta - \dot{u} &= -X_u u_g \end{aligned} \quad (1)$$

The left side of the equations defines the aircraft transient response (aircraft dynamics). The right side defines the manner in which control inputs (δ_e) and turbulence (u_g) force aircraft motion. The equations have been normalized with mass and moment of inertia. These equations were used to develop the transfer functions for the closed-loop model and were also programed on the analog computer for use with the flight simulator.

The stability derivatives M_u (generally presented as $M_{\dot{u}}g$), M_q , and X_u all affect the aircraft's dynamic response. Also, the small rapid changes in airspeed resulting from turbulence cause pitching motion through $M_{\dot{u}}g$ and displacement from the hovering position through X_u . Pitch rate damping, M_q , reduces the aircraft's pitch response to turbulence by developing a restoring moment proportional to pitch rate. Pitch attitude stabilization, M_θ , causes a restoring moment proportional to pitch attitude. Longitudinal control sensitivity, M_{δ_e} , is the initial pitch acceleration per inch of control stick displacement.

SIMULATION OF TURBULENCE.—The simulated turbulence was generated by passing the output of a low-frequency random noise generator through a first-order filter with a break-point frequency, ω_B , of 0.314 rad/sec. The resulting simulated turbulence had a power spectral density given by

$$S_{u_g}(\omega) = \frac{2\omega_B \sigma_{u_g}^2}{\omega_B^2 + \omega^2} \quad (2)$$

where σ_{u_g} is the root mean square value of the random component of the wind. A value of $\sigma_{u_g} = 5.14$ ft/sec was used for this study. In a previous flight test study (ref. 5) the pilots described simulated turbulence having these characteristics as corresponding to a medium-turbulent day.

UARL V/STOL AIRCRAFT FLIGHT SIMULATOR.—This is a full-scale fixed-base Sikorsky S-61 helicopter cockpit with a Norden Contact Analog display system (ref. 4). Aircraft pitch attitude was controlled with a conventional helicopter control stick having inertia but no force gradient. Longitudinal position was controlled through longitudinal forces produced by changing pitch attitude to rotate the thrust vector. .

The Norden Contact Analog system provided the pilot with an inside-out video picture of the motion of the aircraft relative to the Earth and sky. Figure 1 shows the display (ref. 4). The cross indicated the position of the nose of the aircraft relative to the ground and hence remained fixed at the center of the viewing screen. The square traversed

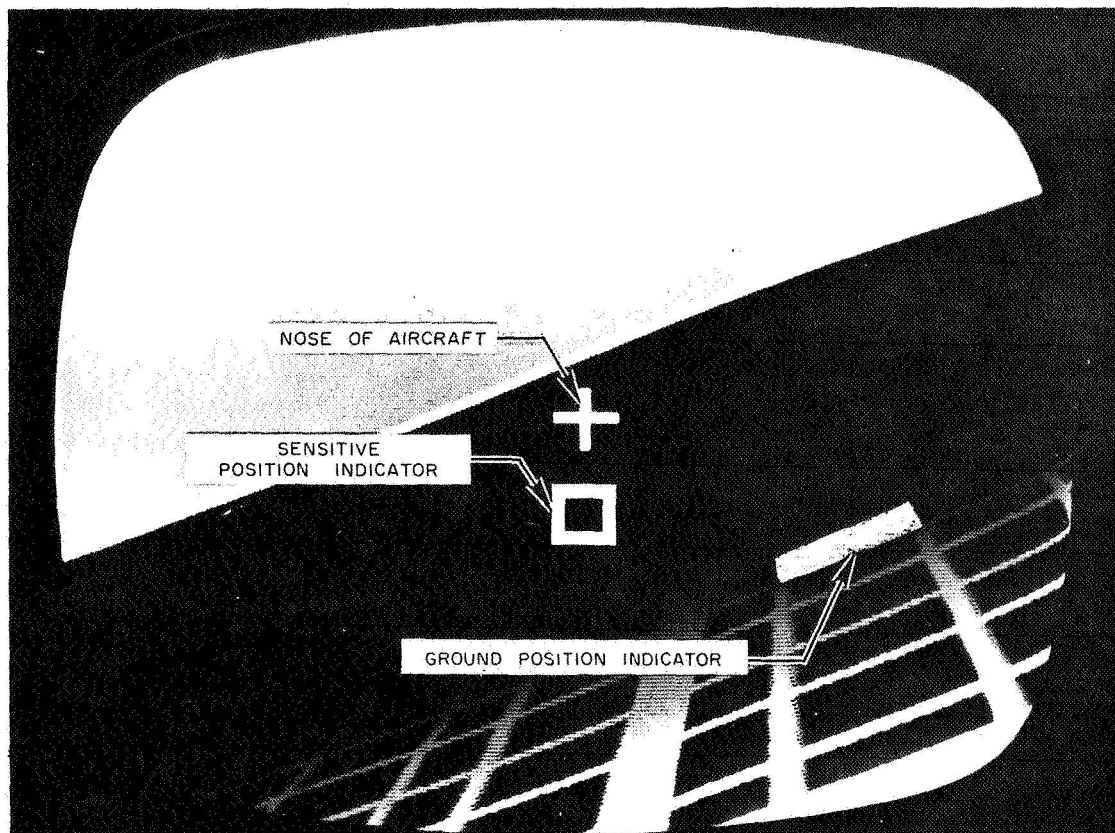


Figure 1.—Contact analog display for hovering and low-speed maneuvering task.

vertically on the screen and was used as a sensitive longitudinal position indicator. When the square was at the center of the screen (coincident with the cross) the aircraft was located directly over a reference hovering position such that the ground position indicator (also shown in fig. 1) was approximately 135 feet ahead of the aircraft for the simulated hovering altitude of 40 feet. Figure 1 shows the aircraft rolled to the right. The roll-attitude and lateral-position degrees of freedom were included in the simulated task. However, the roll motion was well damped and lateral disturbances caused by turbulence were small so that little effort was required to control roll attitude and lateral position (lateral control characteristics were not changed for any part of the study). As a result, the presence of the lateral degrees of freedom did not appreciably affect the pilot's performance of the longitudinal hovering task.

INSTRUCTIONS TO PILOT.—The pilot performed the hovering task for ranges of values of M_{ug} , M_q , and M_θ . The ranges investigated were:

$$0 \leq M_{ug} \leq 1.00$$

$$-5 \leq M_q \leq -1$$

$$-5 \leq M_\theta \leq 0$$

For each configuration the pilot was instructed to select the value of control sensitivity that he considered optimum for the hovering task. Control sensitivity was adjusted using a lever in the cockpit. He then attempted to minimize longitudinal hovering position error; i.e., he attempted to keep the cross centered in the square. The pilot performed this task for 10 periods of 100-second duration each. For each of these periods, rms longitudinal hovering position error, σ_x , rms longitudinal velocity, σ_u , rms pitch attitude, σ_θ , and rms pitch rate, σ_q , were measured. As will be discussed later, these rms values were subsequently used in computing the adapted pilot model parameters.

HOVERING TASK MODEL AND METHOD FOR COMPUTING PILOT ADAPTATION

This section contains a brief description of the model used in the analysis of pilot adaptation and the method for computing pilot adaptation. The mathematical representation for the human pilot, the assumed loop-closure structure and the computation of changes in pilot adaptable parameters from measured data are discussed. More detailed discussions of the implications of the task model and the technique used to compute pilot adaptation are contained in reference 4.

HUMAN PILOT MODEL.—The models selected to represent the human pilot dynamics (fig. 2) were linear except for a pure time delay or transport lag. These quasi-linear models have been found to be realistic approximations for pilot dynamic behavior in both one- and two-degree-of-freedom compensatory tracking tasks (refs. 1 and 2). The hovering task is also a compensatory tracking task. Thus, the basic form of these models can be used here as well.

The form of quasi-linear pilot model used for the pitch loop (fig. 2) is given in Laplace transform notation by

$$Y_{P_\theta} = \frac{K_{P_\theta} (T_{L_\theta} s + 1) e^{-\tau_\theta s}}{T_N s + 1} \quad (3)$$

Pilot gain, K_{P_θ} , represents the magnitude of the pilot's control response to a unit error in the magnitude of the controlled variable. The lead term, $(T_{L_\theta} s + 1)$, is an indication of the pilot's control response to the rate of change in error of the controlled element variable. The parameters K_{P_θ} and T_{L_θ} are adaptable. When the aircraft dynamics and/or turbulence characteristics change, the pilot can adapt his gain and lead to the new conditions. Through this adaptation the pilot can generally maintain stable control and achieve desirable system response characteristics. The model factors $1/(T_N s + 1)$ and $e^{-\tau_\theta s}$ define inherent pilot lags. The transport lag constant, τ_θ , represents an accumulation of delays involved in the transmission of information and decisionmaking. The other lag constant, T_N , is an approximation to the neuromuscular lag involved in the response of the arm to a command. The neuromuscular lag term is only present in the pitch-loop pilot model. This is because the series-loop model assumes that arm activity is restricted to the pilot's control of the pitch loop. These lags and limitations in the assumption that they are constant are discussed in more detail in references 1 and 4. The values used in this study for these constants, which were chosen from the results of reference 1, were $\tau_\theta = 0.09$ second and $T_N = 0.35$ second. The position-loop pilot model (fig. 2) contains terms similar to those described for the pitch-loop pilot model. However, as indicated previously, the only lag term present in Y_{P_x} is that resulting from the transport lag constant, τ_x .

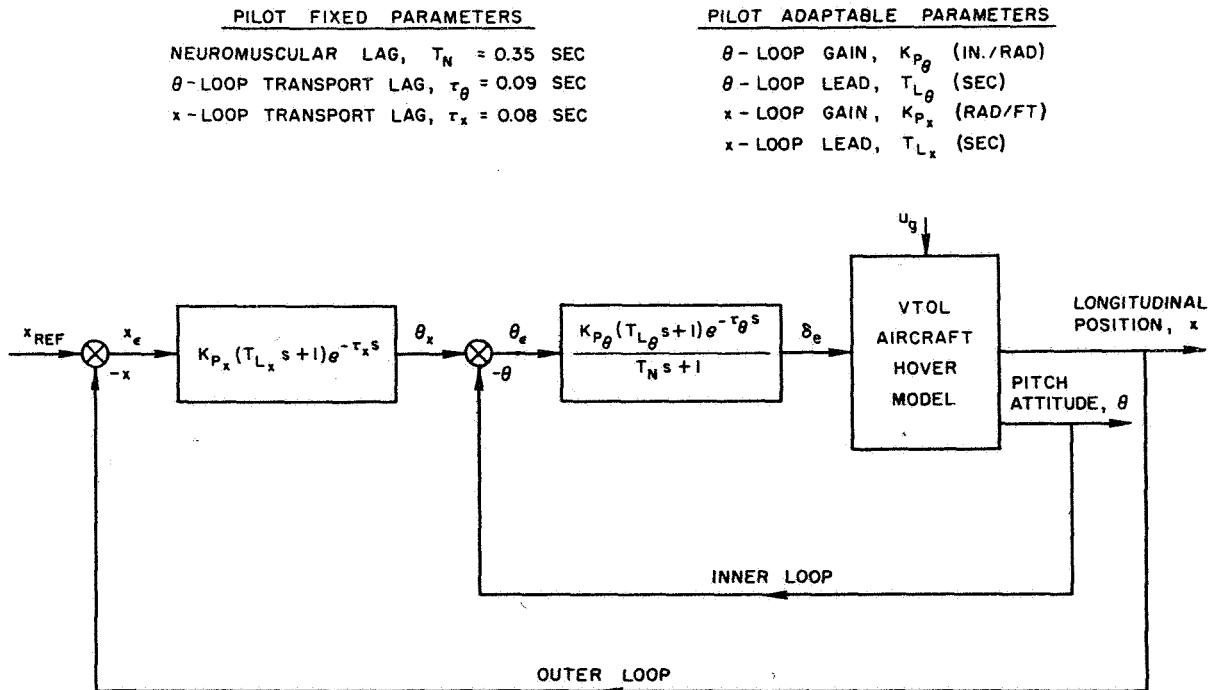


Figure 2.—Series-loop model for pilot control in hover.

LOOP-CLOSURE STRUCTURE.—The model in figure 2 assumes that the pilot closes the pitch (inner) loop and position (outer) loop in series. The pilot observes his hovering position error and mentally generates a pitch-attitude command angle, θ_x . The attitude command angle is that angle which will provide the component of horizontal thrust needed to develop the longitudinal acceleration required for reducing position error. This attitude angle command is routed to the pitch loop. There the pilot model develops a control input, δ_e , to null the error between the commanded attitude angle and the actual pitch attitude.

The series-loop closure is based upon observed pilot behavior. It was selected after first considering a parallel-loop closure model. The basic form of the series model was first brought to the attention of UARL by R. L. Stapleford of Systems Technology, Inc. An identical model has been developed at MIT for a helicopter longitudinal two-degree-of-freedom hovering task (ref. 6).

COMPUTATION OF PILOT-ADAPTED PARAMETERS.—Previous studies concerned with the development of quasi-linear pilot models (refs. 1 and 2) have involved measurement of random-excitation describing functions to represent pilot dynamic behavior. This permits evaluation of (1) the effect of the controlled element dynamics on the pilot's adaptation, (2) the extent to which he is a linear operator, and (3) the effects of his inherent nonlinearities on his adaptation. However, the procedures necessary to compute describing functions are complex and costly, especially for multi-degree-of-freedom control tasks.

Since the objectives of the UARL study were limited to determining trends in pilot-adapted parameters with changes in aircraft stability parameters, a different method was developed. It involves iterating on the pilot-model adapted parameters until the computed rms performance of the closed-loop hovering model matches measured rms hovering performance data obtained in the flight simulator studies. Details of the computational method, its verification, and the hovering task model equations are contained in reference 4. In addition, reference 4 contains a discussion of the effects of pilot nonlinearities on the computed parameters and how these effects are reduced by judicious selection of the rms performance data.

EFFECT OF AIRCRAFT STABILITY PARAMETERS ON PILOT ADAPTATION

Results are presented here to show the effects of changes in the aircraft speed-stability, pitch rate damping, and pitch attitude stabilization parameters on pilot adaptation. These parameters influence the aircraft dynamic response to control inputs and its pitch attitude response to the simulated turbulence. For each parameter, these response characteristics are presented in the frequency domain. The corresponding rms data from the flight simulator are also presented to show the effect of these parameters on hovering performance. The computed loop-closure characteristics (crossover frequencies and phase margins) and the pilot-adapted parameters needed to match the measured rms hovering performance data are discussed. In this paper, only the pitch-loop adaptation characteristics are discussed in detail. Also, results are presented for only one pilot. Data for a second pilot are presented in reference 4; the trends of the corresponding computed model parameters agree well with the results presented here.

LONGITUDINAL SPEED-STABILITY PARAMETER, M_{ug}

Open-Loop Response to Turbulence and Control Inputs.—Four values of longitudinal speed-stability parameter ($M_{ug} = 0, 0.33, 0.67, \text{ and } 1.00$) were considered. The other longitudinal stability derivatives remained unchanged. The simulated turbulence was filtered by the aircraft dynamics before it affected pitch attitude (fig. 2). This filtering effect can be described by $\theta(s)/u_g(s)$, the transfer function relating pitch attitude response to turbulence. The general form of this transfer function is given by

$$\frac{\theta(s)}{u_g(s)} = \frac{M_{ug}s}{\left(s + \frac{1}{T}\right)(s^2 + 2\zeta\omega_n s + \omega_n^2)} \quad (4)$$

The specific forms (in terms of the aircraft stability parameters) of all transfer functions discussed herein are given in table 1. As M_{ug} increases, the gain of $\theta(s)/u_g(s)$ at frequencies greater than 0.4 to 0.5 rad/sec increases (fig. 3a) and the pilot is required to attenuate disturbances having larger amplitudes. This, of course, increases the difficulty of the hovering control task. Note in equation (4) that for $M_{ug} = 0$, aircraft pitch attitude does not respond to turbulence.

Variations in M_{ug} also affect the aircraft's pitch response to control inputs, i.e., the dynamics of the controlled element change. The pitch response to control inputs is given by

$$\frac{\theta(s)}{\delta_e(s)} = \frac{M_{\delta_e}(s - x_u)}{\left(s + \frac{1}{T}\right)(s^2 + 2\zeta\omega_n s + \omega_n^2)} \quad (5)$$

For $M_{ug} = 0$, the denominator has three real roots (table 1). As M_{ug} increases, the roots change to a real root and a complex-conjugate pair. The natural frequency, ω_n , increases with M_{ug} and the damping ratio, ζ , decreases and becomes negative at $M_{ug} = 1.00$. The frequency response characteristics for control inputs $\theta(s)/\delta_e(s)$ are shown in figure 4 (M_{δ_e} has a constant value of 1.0 for these plots).

Measured RMS Hovering Performance.—An indication of the pilot's ability to adapt to changes in attitude disturbances and controlled element characteristics can be obtained by examining his rms hovering performance in the flight simulator experiments. These data are shown in figure 5a. Note that the pilot's rms hovering position error, hovering velocity, and pitch attitude changed very little with changes in M_{ug} . However, rms pitch rate increased with increasing M_{ug} . These results indicate that the pilot could attenuate relatively well the increased pitch attitude response to turbulence at the high values of M_{ug} . However, the pilot did not generate the increased pitch-loop lead (which will be shown later) needed to prevent an increase in rms pitch rate.

Loop-Closure Characteristics.—Specific closed-loop response and stability characteristics are required to match measured rms performance data with rms values computed from the equations for the series-loop model. The model closed-loop performance characteristics must correspond to those used by the human pilot in the performance of the task. A simplified means of describing the response and stability characteristics of the closed-loop

TABLE 1.—VTOL AIRCRAFT OPEN-LOOP LONGITUDINAL TRANSFER FUNCTIONS

Specific Form of the Transfer Functions	Configurations				Factors of Δ_1	
	M_q	X_u	$M_u g$	M_θ	$s + 1/T$	$s + \zeta\omega_n \pm j\omega_d$
Δ_1	-3	-0.1	0.0	0	$s + 3.0$	$s + 0.10$
			0.33		$s + 3.0$	$s + (0.03 \pm j 0.33)$
			0.67		$s + 3.1$	$s + (0.01 \pm j 0.47)$
			1.00		$s + 3.1$	$s - (0.004 \pm j 0.57)$
e/δ_e						
e/u_g	-1	-0.1	0.67	0	$s + 1.4$	$s - (0.14 \pm j 0.68)$
	-3				$s + 3.1$	$s + (0.013 \pm j 0.47)$
x/δ_e	-5				$s + 5.0$	$s + (0.04 \pm j 0.36)$
x/u_g	-1	-0.05	1.00	0	$s + 1.5$	$s - (0.21 \pm j 0.80)$
				-3	$s + 0.4$	$s + (0.32 \pm j 1.64)$
				-5	$s + 0.3$	$s + (0.40 \pm j 2.18)$

$$\Delta_1 = s^3 - (M_q + X_u)s^2 + (M_q X_u - M_\theta)s + (M_u g + M_\theta X_u)$$

$$e/\delta_e = \frac{M_{\delta_e} (s - X_u)}{\Delta_1}$$

$$e/u_g = \frac{M_u s}{\Delta_1}$$

$$x/\delta_e = \frac{-M_{\delta_e} g}{s\Delta_1}$$

$$x/u_g = \frac{X_u (s^2 - M_q s - M_\theta) - M_u g}{s\Delta_1}$$

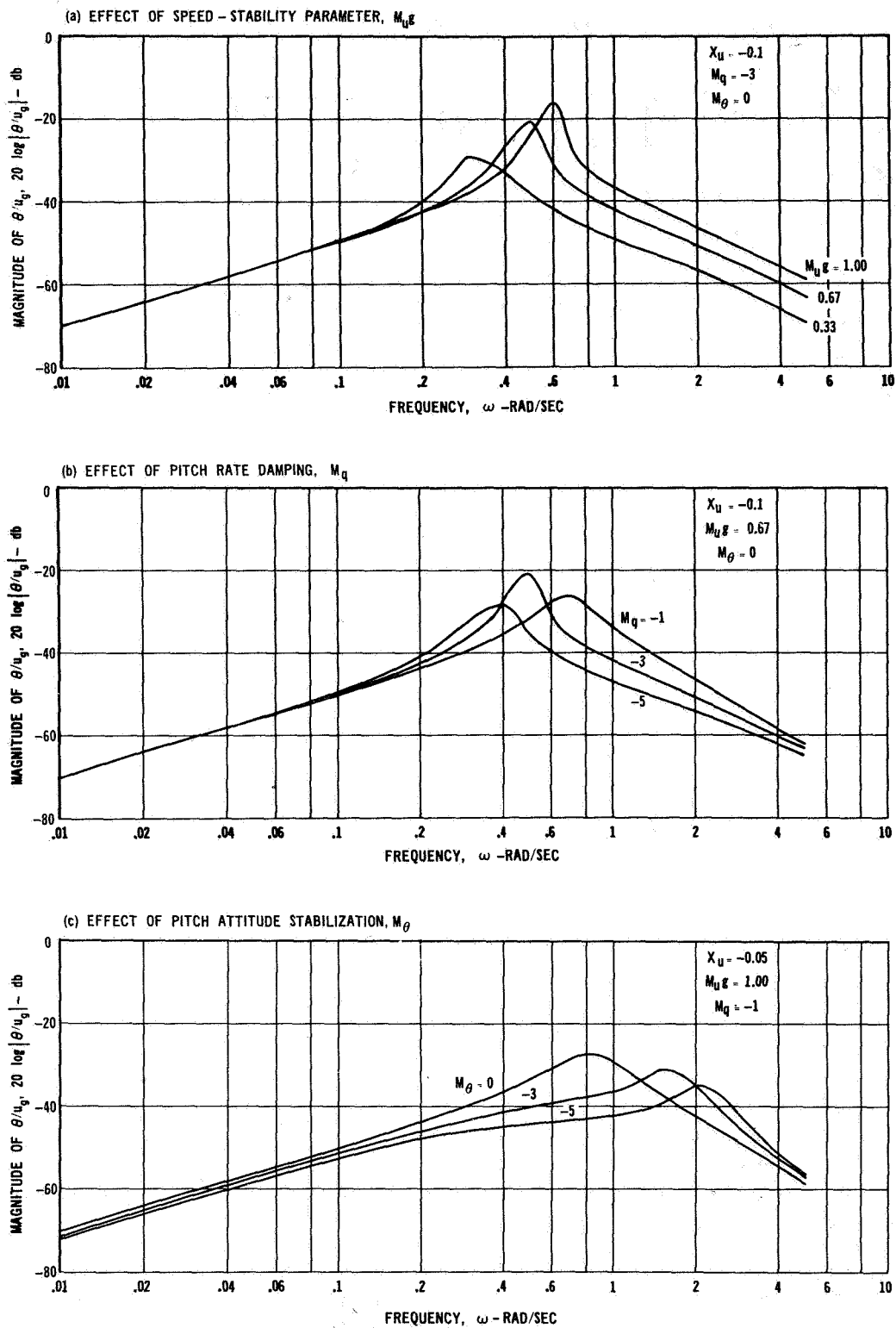


Figure 3. —Effect of aircraft stability parameters on the magnitude of the transfer function relating pitch attitude response to turbulence, $\theta(s)/u_g(s)$.

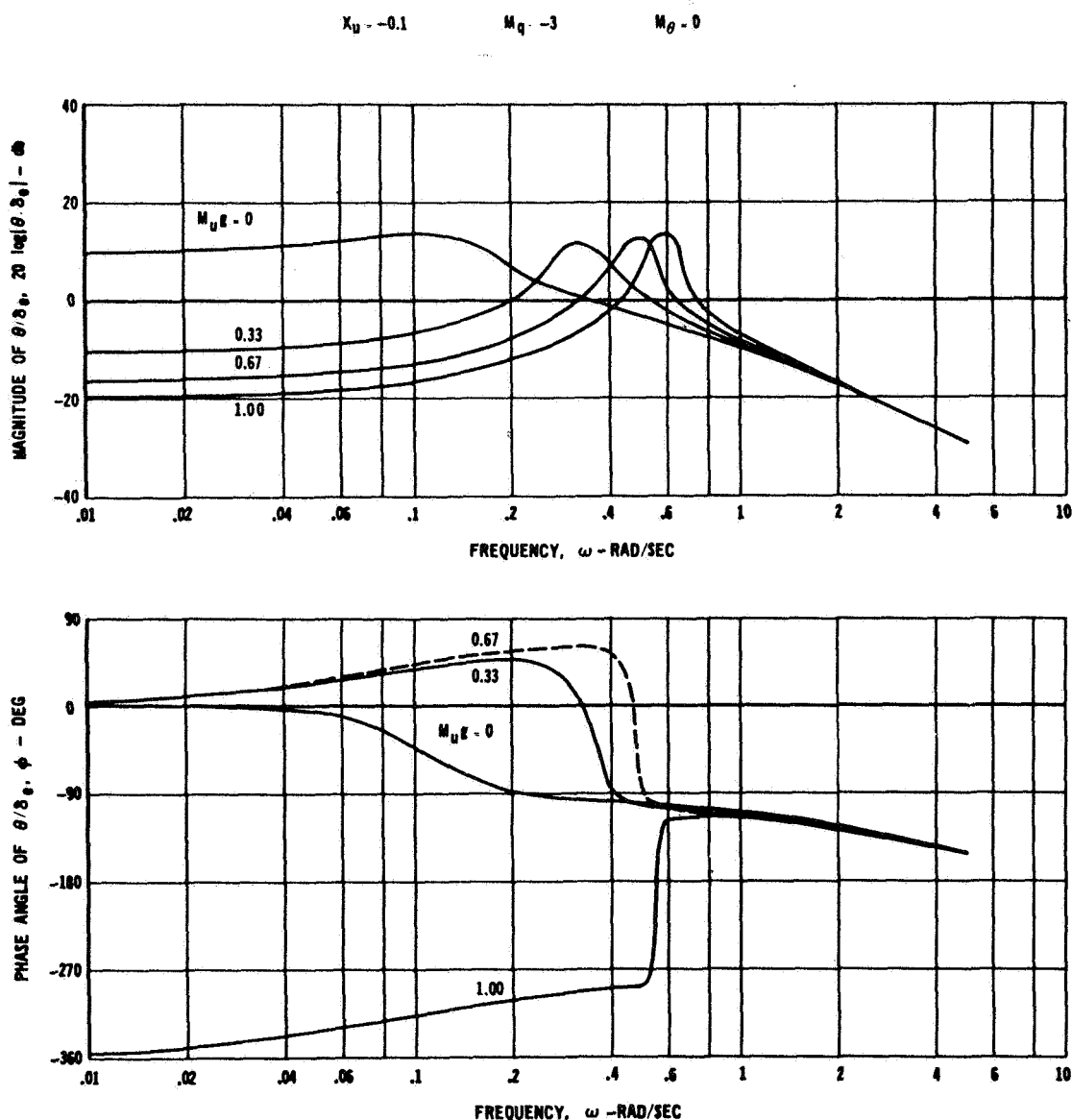


Figure 4.—Effect of speed stability parameter on the frequency response of the transfer function relating pitch attitude to control inputs, $\theta(s)/\delta_e(s)$.

model utilizes the crossover frequencies¹ (ω_c) and phase margins² (PM) of the two loops. The pitch-loop crossover frequency, $\omega_{c\theta}$, and phase margin, PM_θ , are obtained from Bode plots of the product of the transfer functions of the control elements included in the pitch attitude loop, $Y_{P\theta}(s)$ and $\theta(s)/\delta_e(s)$.

¹Pitch-loop crossover frequency, $\omega_{c\theta}$, is that frequency at which the gain of the product $Y_{P\theta}(s) \cdot \theta(s)/\delta_e(s)$ is equal to 1; increasing crossover frequency generally implies faster response to errors.

²Pitch-loop phase margin, PM_θ , is the sum of the phase angle of the aircraft open-loop dynamics and the phase angle of the pilot model at the crossover frequency, plus 180° ; the larger the phase margin the more stable the closed-loop response.

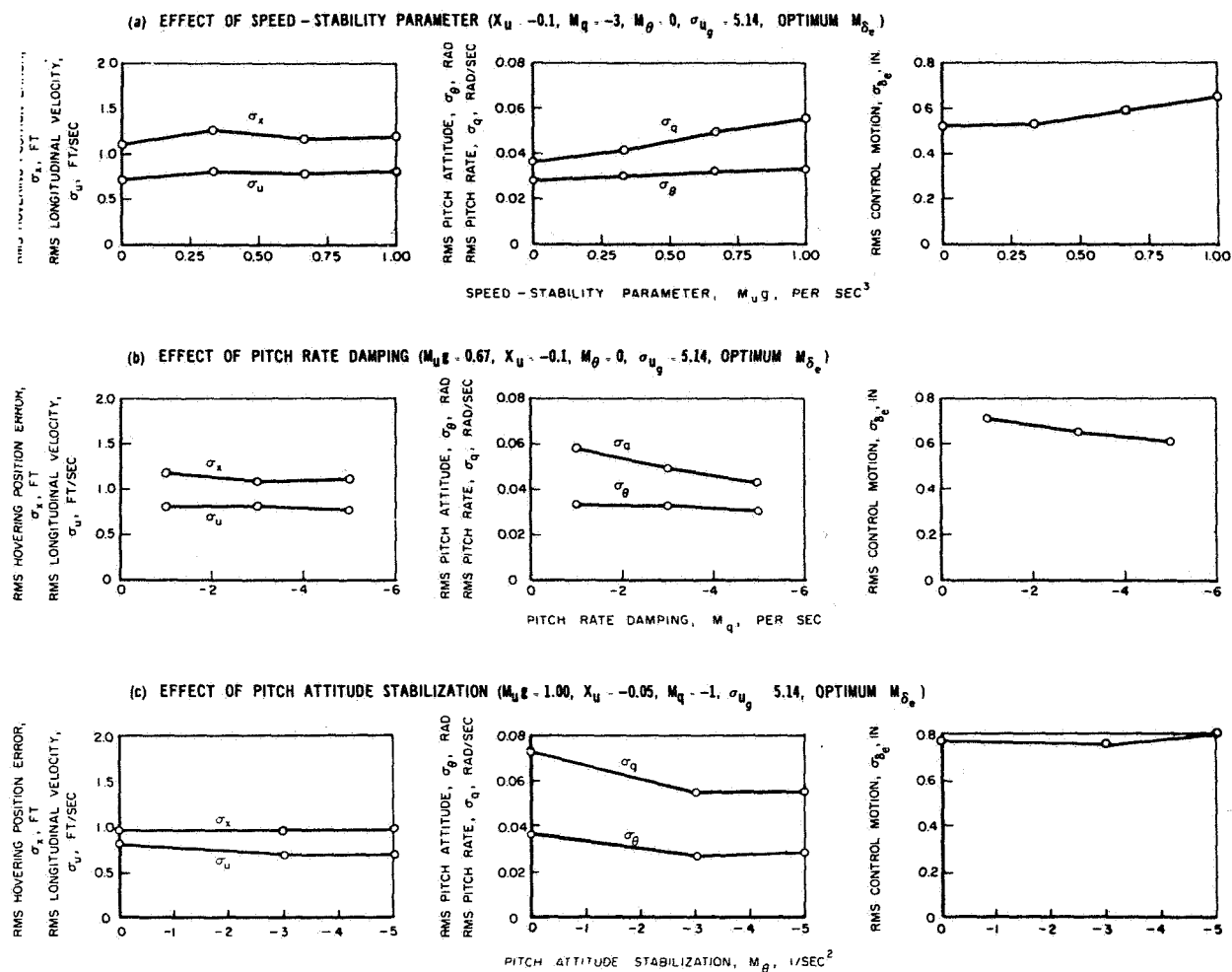


Figure 5. — Effect of aircraft stability parameters on RMS hovering performance.

Bode plots of $Y_{P_\theta}(s) \cdot \theta(s) / \delta_e(s)$ derived from the measured rms performance data for four values of M_{u_g} are shown in figure 6. Crossover frequency increased with increasing M_{u_g} . This is a reflection of the pilot's desire for faster pitch-loop response when attitude disturbances increased in amplitude at the higher frequencies (fig. 3a). The crossover frequencies and phase margins for the pitch and position loops were:

M_{u_g}	ω_{c_θ}	PM_θ	ω_{c_x}	PM_x
0	2.2	7	0.9	19
0.33	2.7	7	0.9	18
0.67	3.0	9	1.0	19
1.00	3.2	10	1.0	21

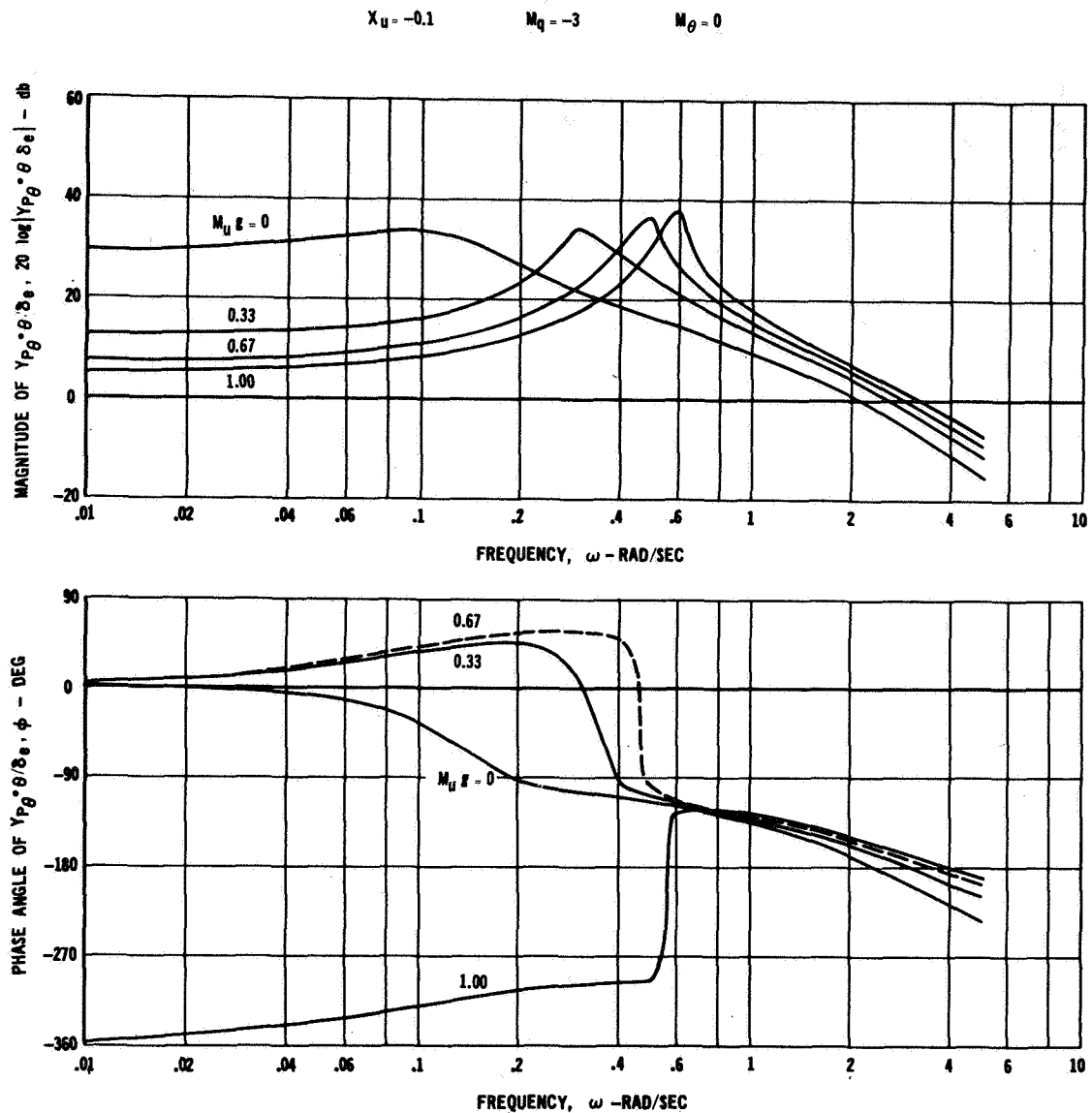


Figure 6.—Effect of speed stability parameter on the frequency response of the transfer function relating pitch attitude to pitch error, $Y_{P_\theta}(s) \cdot \theta(s) / \delta_e(s)$.

Note in figure 4 that the resonant frequency of $\theta(s) / \delta_e(s)$ increased with increasing M_{Ug} while the low-frequency gain ($\omega < 0.2$ rad/sec) decreased. Larger values of lead and gain had to be adapted in the pitch-loop pilot model at the higher values of M_{Ug} to supplement the response of $\theta(s) / \delta_e(s)$ and to provide the loop-closure characteristics. In terms of the actual pilot performance of the task, these computed loop-closure results imply that as M_{Ug} increased the pilot responded with larger control inputs for a given magnitude of observed error in pitch attitude. They also indicate that he correspondingly increased his anticipation of the effects of attitude rates to retain stability (increased ω_{c_θ} with nearly constant PM_θ). It should also be pointed out that for $M_{Ug} = 0$ (i.e., no pitch attitude disturbances due to

turbulence), $\omega_{c\theta}$ did not approach zero. Here, the pilot did not need the pitch control response to attenuate pitch disturbances, but he still had to command pitch attitude changes to reduce hovering position errors induced by position disturbances.

The closure of the position loop does not appear to have changed significantly with changes in M_{ug} . The small increase in ω_{cx} probably reflects the need for slightly faster position-loop response to attenuate the position-loop disturbances resulting from the small increase in σ_θ at the higher values of M_{ug} (fig. 5a).

Pilot-Adapted Parameters. —The pilot-adapted parameters required to provide the previously described loop-closure characteristics are shown in the following tabulation. The control stick sensitivity is also tabulated because it could be selected by the pilot, and thus can be considered an adaptable parameter.

$\underline{M_{ug}}$	$\underline{M_{\delta_e}}$	$\underline{K_{p\theta}}$	$\underline{M_{\delta_e} \cdot K_{p\theta}}$	$\underline{T_{L\theta}}$	$\underline{K_{px}}$	$\underline{T_{Lx}}$
0	0.300	32.6	9.8	0.006	-0.017	0.72
0.33	0.360	38.6	13.9	0.10	-0.020	0.53
0.67	0.431	37.7	16.2	0.16	-0.024	0.49
1.00	0.481	36.7	17.6	0.20	-0.0251	0.50

Adapted pitch-loop gain, $K_{p\theta}$, remained nearly constant with changes in M_{ug} while the pilot increased M_{δ_e} . Thus, the product $M_{\delta_e} \cdot K_{p\theta}$ increased with M_{ug} , indicating the need to increase the gain of $\theta(s)/\delta_e(s)$ and provide the pitch-loop crossover frequency required to suppress the high-frequency gust disturbances.

Low-frequency gain was required in the pitch loop to provide response to low-frequency pitch-attitude commands generated from observed errors in hovering position (θ_x in fig. 2). However, a large low-frequency gain was not an overriding requirement; in figure 6 it is shown that the pilot allowed the low-frequency gain of $Y_{p\theta} \cdot \theta(s)/\delta_e(s)$ to decrease with increasing M_{ug} while he increased $\omega_{c\theta}$.

These results also indicate that the pilot's criterion for selecting control sensitivity was to keep his internal gain constant while increasing the pitch-loop crossover frequency. That is, adapted $K_{p\theta}$ remained nearly constant while the pilot increased M_{δ_e} .

Adapted pitch-loop lead was small for all values of M_{ug} because the controlled element was relatively stable. The transient response from the first-order root in the controlled element dynamics dissipated quickly while the lightly damped oscillatory transient response was of such low frequency that the pilot had no difficulty in controlling it (table 1). The small increase in $T_{L\theta}$ with M_{ug} was necessary to extend $\omega_{c\theta}$ and maintain closed-loop stability at the higher values of M_{ug} .

The pilot's adapted position-loop parameters also changed with M_{ug} . However, these are more a reflection of the changes in the adapted pitch-loop parameters than an indication of any significant change in response to position-loop disturbances. This is because in the series-loop model the pitch-loop adapted parameters (M_{δ_e} , $K_{p\theta}$, $T_{L\theta}$) also affect the computed pilot response to position-loop disturbances (ref. 4). Consequently, if the pilot increases his response to pitch attitude disturbances (increasing $M_{\delta_e} \cdot K_{p\theta}$ and/or $T_{L\theta}$), and does not change his response to position disturbances, the computed position-loop adaptable parameters will change.

PITCH RATE DAMPING, M_q

Open-Loop Response to Turbulence and Control Inputs.—The effect of M_q on the Bode plots of the transfer function relating open-loop attitude response to turbulence is shown in figure 3b. As M_q becomes more negative, the resonant frequency decreases and the amplitude of the high-frequency ($\omega > 0.4$ to 0.5) attitude disturbances also decreases. For more negative M_q , then, aircraft pitch attitude becomes less responsive to high-frequency components of turbulence. The low-frequency attitude response to turbulence ($\omega < 0.2$) was essentially unchanged with changes in M_q . The effect of M_q on the roots is given in table 1.

The effect of M_q on the transfer function relating aircraft response to control inputs is shown in figure 7. The roots in table 1 show that the damped frequency of the aircraft dynamics decreases with more negative M_q while the damping ratio increases. The aircraft open-loop dynamics are unstable at $M_q = 1$, but stable at $M_q = -3$ and -5 . As shown in figure 7, at low frequencies ($\omega < 0.1$) the gain of $\theta(s)/\delta_e(s)$ is nearly invariant with M_q (again, M_{δ_e} has a value of 1.0 in these plots).

Measured RMS Hovering Performance.—The effect of M_q on rms hovering performance is shown in figure 5b. As was the case for changes in M_{ug} , σ_θ , σ_x , and σ_u were essentially unaffected by changes in M_q . The pilot was able to adapt to the lightly damped dynamics of the controlled element at $M_q = -1$, where the attitude response to turbulence at high frequencies was greatest, and to perform the hovering task as well as for the more heavily damped and stable dynamics at $M_q = -5$. Here, as in the case of changes in M_{ug} , the pilot was able to adapt to the variations in disturbance level and controlled element dynamics and maintain relatively constant hovering performance. Again, σ_q was largest for the most gust-sensitive configuration ($M_q = -1$). The pilot did not generate the pitch-loop lead ($T_{L\theta}$) necessary to maintain constant σ_q because he could maintain constant hovering performance without doing so.

Loop-Closure Characteristics.—The frequency response characteristics of the transfer function relating open-loop pitch attitude response to the observed pitch error, $Y_{P\theta} \cdot \theta(s)/\delta_e(s)$, are shown in figure 8 for the three values of M_q . The crossover frequencies and phase margins of this transfer function are shown in the following tabulation:

M_q	ω_{c_θ}	PM_θ	ω_{c_x}	PM_x
-1	3.4	8	1.0	21
-3	3.1	8	1.0	15
-5	2.8	10	1.0	19

As M_q became more negative and the pitch dynamics became more stable and less responsive to turbulence, pitch-loop crossover frequency decreased while the position-loop closure characteristics were essentially unchanged. As in the case of M_{ug} , the model results indicate that the pilot's main concern was to adjust his adaptable parameters to provide adequate response to the high-frequency disturbances ($\omega > 0.4$). He appeared less concerned about the low-frequency ($\omega < 0.3$) open-loop pitch gain because he tolerated significant differences in low-frequency gain for the different values of M_q . These results again would appear to reflect the relative unimportance of the low-frequency attitude disturbances.

However, some low-frequency gain and also some minimum value of pitch-loop crossover frequency must be maintained to provide acceptable pitch attitude response to correct for errors in hovering position.

The position-loop crossover frequency did not change with M_q . The pilot compensated for changes in pitch-loop characteristics in such a way that rms pitch attitude error remained nearly constant. Therefore, the contribution to the rms position error from the rms pitch attitude error was also nearly constant. The pilot therefore did not have to make any significant adjustments in his position-loop closure.

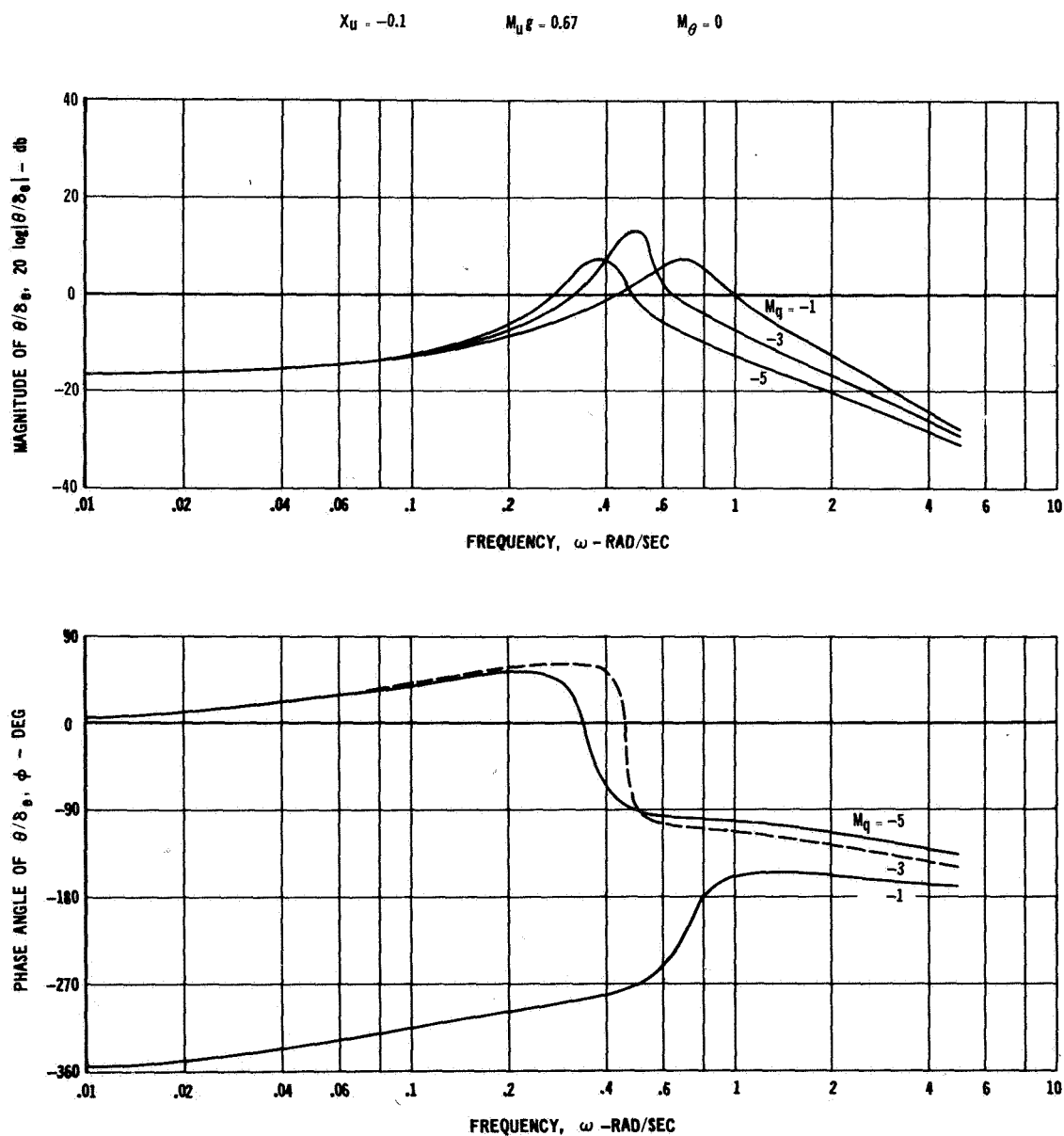


Figure 7.—Effect of pitch rate damping on the frequency response of the transfer function relating pitch attitude to control inputs, $\theta(s)/\delta_e(s)$.

Pilot-Adapted Parameters.—The pilot-model adaptable parameters are shown in the following tabulation:

$\underline{M_q}$	$\underline{M_{\delta e}}$	$\underline{K_{P_\theta}}$	$\underline{K_{P_\theta} \cdot M_{\delta e}}$	$\underline{T_{L_\theta}}$	$\underline{K_{P_x}}$	$\underline{T_{L_x}}$
-1	0.369	25.6	9.45	0.50	0.028	0.41
-3	0.431	39.7	17.06	0.17	0.027	0.41
-5	0.493	43.8	21.59	0.05	0.023	0.56

The product of pilot adaptable gain and control stick sensitivity, $K_{P_\theta} \cdot M_{\delta e}$, increased as M_q became more negative. This was required to compensate for the reduction in gain of $\theta(s)/\delta_e(s)$ at frequencies greater than about 0.4 rad/sec. The pilot adapted this increased gain even though the pitch attitude response to turbulence at these frequencies decreased. It should again be pointed out, however, that ω_{c_θ} became smaller as M_q became more negative. Also, the adapted gain resulted in larger low-frequency gain for $Y_{P_\theta} \cdot \theta(s)/\delta_e(s)$ as M_q became more negative (fig. 8). As discussed previously, the effects of M_q on the low-frequency characteristics of $\theta(s)/u_g(s)$ (the gain was small and remained unchanged with changes in M_q) and $\theta(s)/\delta_e(s)$ (gain also remained unchanged with changes in M_q) do not require the pilot to adjust his low-frequency gain. The effect of M_q on the low-frequency gain of $Y_{P_\theta} \cdot \theta(s)/\delta_e(s)$ would seem to be simply a result of the response requirements at higher frequencies and the form of the pilot model.

Adapted pitch-loop lead, T_{L_θ} , decreased as M_q became more negative. The controlled element dynamics became more stable as M_q became more negative. That is, the time constant of the first-order root decreased and the damping ratio of the oscillatory roots increased (table 1). The requirements for large ω_{c_θ} also decreased; consequently, there was a diminished requirement for T_{L_θ} .

The adapted position-loop parameters also changed with changes in M_q . However, as in the case of changes in M_{u_g} , these changes reflect the effect of the inner-loop changes rather than a fundamental change in the nature of the pilot's outer-loop control characteristics.

PITCH ATTITUDE STABILIZATION, M_θ

Open-Loop Response to Turbulence and Control Inputs.—The effect of M_θ on the pitch attitude response to turbulence is shown in figure 3c. As M_θ becomes more negative, there is a significant reduction in the frequency response to turbulence up to $\omega = 1$ rad/sec, but an increased response to turbulence at higher frequencies. The resonant frequency of the attitude response to turbulence increases for more negative M_θ but it appears that the over-all level of attitude disturbances (the total power) evident to the pilot is reduced.

The effect of M_θ on $\theta(s)/\delta_e(s)$ is shown in figure 9. The low-frequency gain decreases as M_θ becomes more negative and the high-frequency gain increases. The damped frequency of oscillation also increases as M_θ becomes more negative (table 1). The damping ratio increases when M_θ is changed from 0 (negative damping ratio) to -3, but decreases (remaining positive) again when M_θ is changed to -5. The aircraft dynamics were unstable at $M_\theta = 0$ and stable for $M_\theta = -3$ and -5.

Aircraft pitch attitude is difficult to control for $M_\theta = 0$ because it is unstable and quite responsive to turbulence. At $M_\theta = 0$ the time constant from the real pole is relatively

large and its effects are quite noticeable. Here, however, the damped frequency of the oscillatory pair is small and the transient effects resulting from the oscillatory dynamics are not particularly annoying. As M_θ is made more negative, the time constant of the real pole increases but its effect on aircraft transient response is canceled by the numerator term $(s-X_u)$ so that for $M_\theta = -3$ the dynamics are more easily controlled. However, as mentioned, the damped frequency of oscillation increases when M_θ becomes more negative. For $M_\theta = -5$ the higher oscillatory frequency and reduced damping ratio make control of the oscillatory portion of the dynamics somewhat difficult. The effect of M_θ on the root locations of the transfer functions $\theta(s)/u_g(s)$ and $\theta(s)/\delta_e(s)$ are shown in table 1 ($M_{\delta_e} = 1.0$).

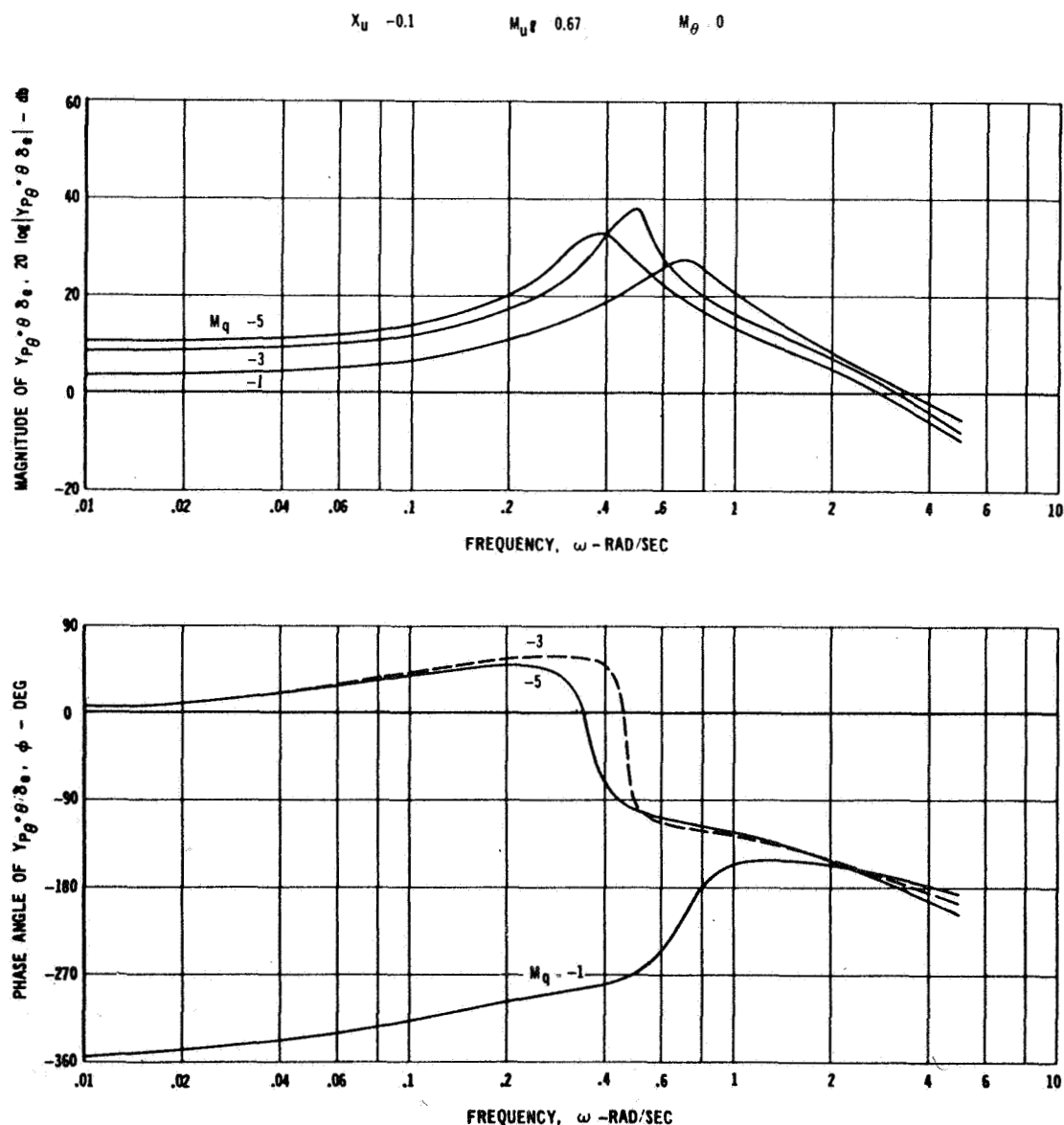


Figure 8. —Effect of pitch rate damping on the frequency response of the transfer function relating pitch attitude to pitch error, $Y_{P_\theta}(s) \cdot \theta(s) / \delta_e(s)$.

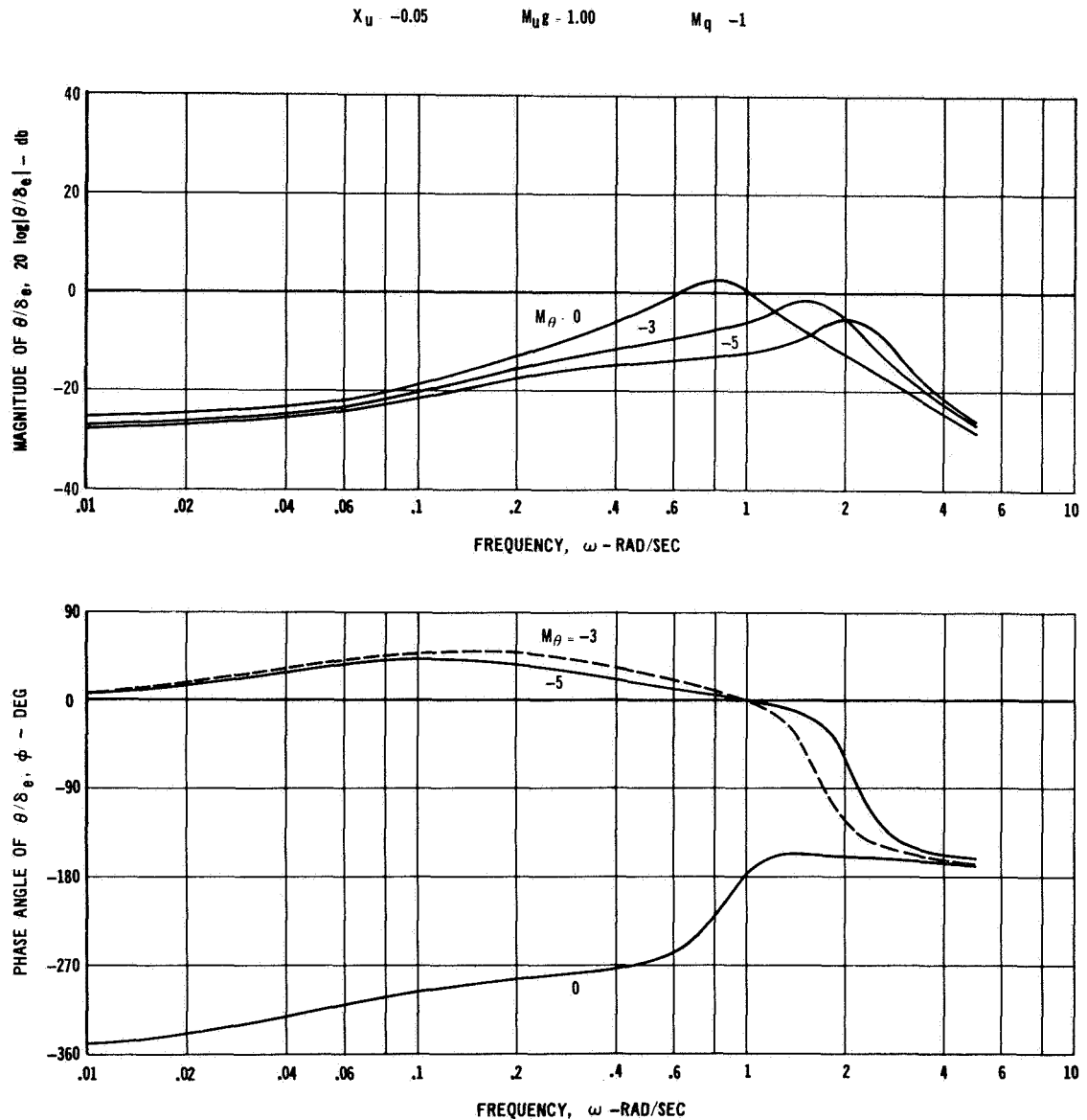


Figure 9.—Effect of pitch attitude stabilization on the frequency response of the transfer function relating pitch attitude to control inputs, $\theta(s)/\delta_e(s)$.

Measured RMS Hovering Performance.—The effect of M_θ on rms hovering performance is shown in figure 5c. As in the cases of changes in M_{ug} and M_q , the rms hovering position error and longitudinal velocity were essentially unaffected by M_θ . There was a reduction in both σ_θ and σ_q when M_θ was changed from 0 to -3. This reduction in σ_θ indicates that the pilot had some difficulty in suppressing attitude disturbances for $M_\theta=0$. When M_θ was made more negative, i.e., $M_\theta=-5$, there was little change in any of the rms values from those measured for $M_\theta=-3$. The effects of M_{ug} , M_q , and M_θ on the measured rms performance were quite similar. Measured values of σ_x and σ_u , and generally of σ_θ also, were relatively unchanged. However, σ_q (and σ_{δ_e}) increased for configurations that were more difficult to control and/or more responsive to turbulence.

Loop-Closure Characteristics.—A Bode plot for the transfer function $Y_{P_\theta}(s) \cdot \theta(s)/u_g(s)$ is shown in figure 10. The low-frequency gain of this transfer function ($\omega < 1$ rad/sec) decreased with more negative values of M_θ , while the high-frequency gain increased somewhat ($\omega > 1.5$ rad/sec). The crossover frequencies and phase margins computed from these plots are:

M_θ	ω_{c_θ}	PM_θ	ω_{c_x}	PM_x
0	3.2	11	1.2	17
-3	3.8	10	1.0	18
-5	3.6	13	1.0	18

Pitch-loop crossover frequency increased as M_θ was changed from 0 to -3 and then decreased slightly for $M_\theta = -5$. Even though the magnitude of the attitude disturbances over most of the frequency range was decreased by the addition of M_θ (fig. 3c), the computed pitch-loop crossover frequency increased. This apparently was an indication of the pilot's desire to attenuate the increased amplitude of the high-frequency disturbances for $M_\theta = -3$ and -5. However, ω_{c_θ} did decrease slightly at $M_\theta = -5$ when the total power of the attitude disturbances was decreased still further. The results shown in figure 10 again point out the apparent unimportance of the low-frequency pitch-loop gain to the pilot. The magnitude of the low-frequency pitch-loop gain to the pilot. The magnitude of the low-frequency attitude disturbances ($\omega < 0.1$) for M_θ differ little from those for M_{u_g} and M_q (fig. 3). However, the low-frequency gain of $Y_{P_\theta} \cdot \theta(s)/u_g(s)$ computed from the M_θ data is significantly lower than those computed using the M_{u_g} and M_q data for the same frequency region. This is consistent with the low amplitude of attitude disturbances over this frequency band. However, it might be supposed that some minimum value of low-frequency pitch attitude gain would be necessary to provide acceptable attitude response to attitude command angles (θ_x in fig. 2) resulting from low-frequency position errors.

Position-loop crossover frequency was slightly larger for M_θ of 0 than for -3 and -5. The pilot did not suppress attitude disturbances as well at $M_\theta = 0$ and as a result they can be assumed to have caused increased position disturbances. Because hovering error did not increase at $M_\theta = 0$, the pilot apparently increased his response in the position loop. This is reflected in the slightly larger ω_{c_x} .

Pilot Adapted Parameters.—Pilot model adaptable parameters for the three values of M_θ are shown in the following tabulation:

M_θ	$M_{\delta e}$	K_{P_θ}	$K_{P_\theta} \cdot M_{\delta e}$	T_{L_θ}	K_{P_x}	T_{L_x}
0	0.403	20.0	8.1	0.54	0.035	0.36
-3	0.352	23.7	8.3	0.56	0.039	0.33
-5	0.327	21.0	6.9	0.50	0.048	0.33

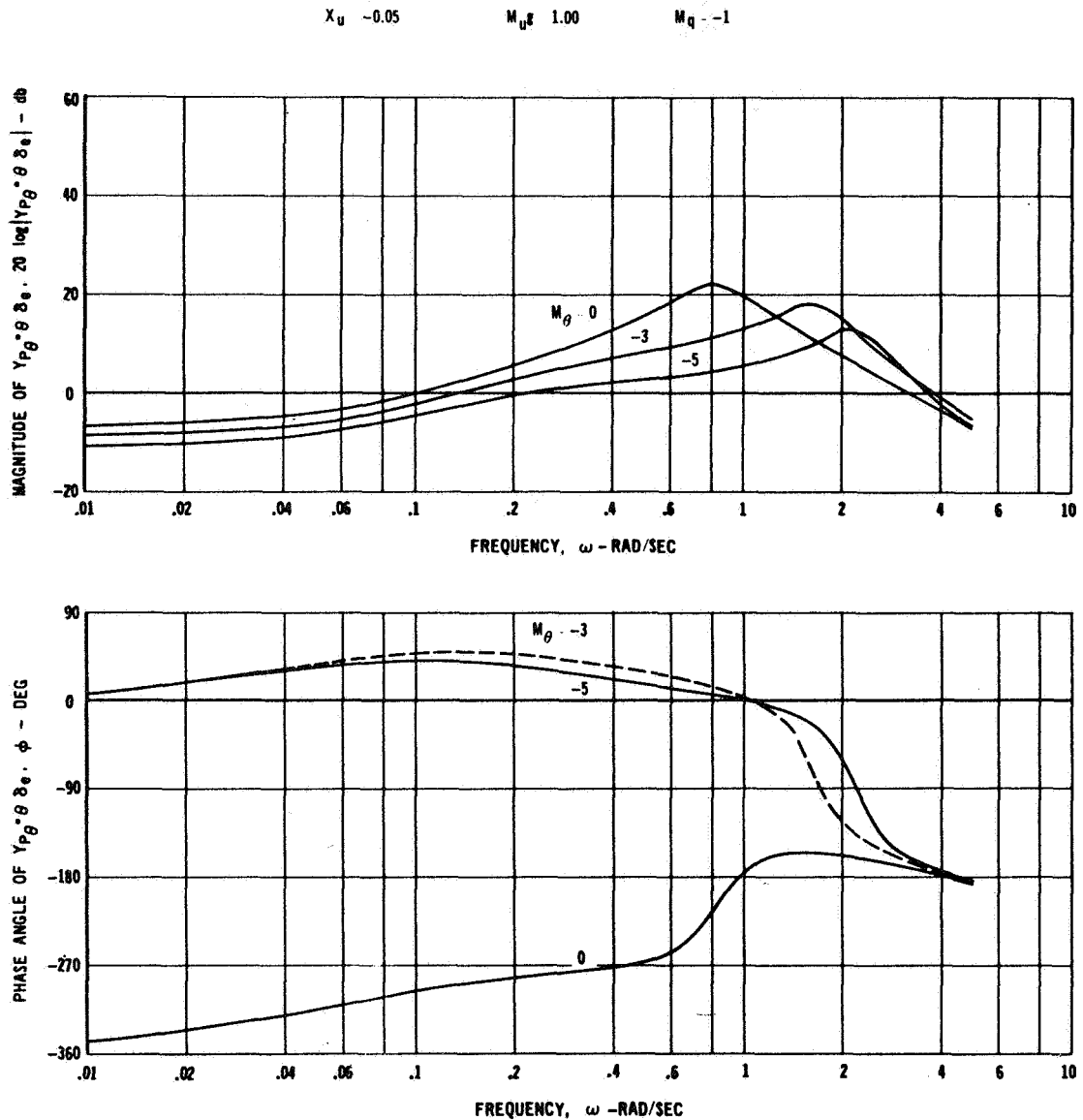


Figure 10.—Effect of pitch attitude stabilization on the frequency response of the transfer function relating pitch attitude to pitch error, $Y_{P_\theta}(s) \cdot \theta(s) / \delta_e(s)$.

The effective pilot gain, $K_{P_\theta} \cdot M_{\delta_e}$ increased slightly and then decreased with M_θ . Again, K_{P_θ} remained relatively constant while the pilot decreased M_{δ_e} . It was pointed out previously (fig. 9) that the addition of M_θ increased the magnitude of $\theta(s) / \delta_e(s)$ at frequencies greater than 1 to 1.5 rad/sec. Therefore, when the pilot adapts nearly the same level of gain for all values of M_θ , control stick sensitivity must be reduced as M_θ becomes more negative. Note, however, that the K_{P_θ} adapted here is only about half that generally adapted during the M_q and $M_{u\theta}$ studies. The reason for this difference is being investigated. As in the case of the M_q and $M_{u\theta}$ studies, it appears that gain $K_{P_\theta} \cdot M_{\delta_e}$ was adapted to provide the crossover frequencies, ω_{c_θ} , needed to control the high-frequency attitude disturbances.

Adapted pitch-loop lead remained essentially unchanged for all values of M_θ . It was also relatively high for all values of M_θ . Even though the aircraft dynamics became more

stable at $M_0 = -3$ (table 1), the pilot was required to extend ω_{c0} to the larger amplitudes of the high-frequency disturbances. As a result the lead requirements remained high because of the need to maintain closed-loop stability. This rationale also applies to the relatively high value of lead adapted for $M_0 = -5$.

The small changes in position-loop adapted parameters are consistent with the small changes in the pitch-loop parameters. These position-loop parameters did not have to change to compensate for the changes in pitch-loop parameters. Thus, they indicate the relatively constant nature of the position-loop closure.

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